HEAT TRANSFER TO THE THROAT REGION OF A SOLID PROPELLANT ROCKET NOZZLE

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HEAT TRANSFER TO THE THROAT REGION OF A SOLID PROPELLANT ROCKET NOZZLE

by
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ABSTRACT: A combined experimental and analytical method for obtaining the surface heat-transfer rate in a rocket nozzle ~~has been~~ developed at the Naval Ordnance Laboratory. This method is particularly applicable to high energy rocket nozzle flow where instrumentation directly on the flow surface is impractical.

The method employs data of the temperature-versus-time history of two points within the nozzle wall with one of them near the surface of the nozzle. The temperature distribution between the two points and the temperature of the nearby nozzle surface are computed on the IBM 7090 using the implicit numerical solution to the one-dimensional transient heat conduction equation. The heat-transfer rate at the nozzle surface is then calculated from the computed temperature gradient at the surface. Application of this method to determine the heat-transfer rate at the throat of a molybdenum insert in a conical solid propellant rocket nozzle is presented. The nozzle was operated at nominal chamber conditions of 1150 psia and 2500°K in the Johns Hopkins University Applied Physics Laboratory rocket tunnel facility. The experimental technique is described.

The experimental data are compared with theoretical predictions and other available experimental results. Good agreement is obtained with turbulent heat-transfer rates computed from the numerical integration of the boundary-layer momentum equation.

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Heat Transfer to the Throat Region of a Solid Propellant Rocket Nozzle

This report presents the results of a program to investigate the applicability of existing heat-transfer theories to the case of heat transfer involving combustion products such as in a solid propellant rocket nozzle.

The author wishes to express his indebtedness to Dr. F. Hill of the John Hopkins University Applied Physics Laboratory whose generosity made the experimental program possible, and to Messrs. E. Wallace and E. Robison, also of the Johns Hopkins University Applied Physics Laboratory for their assistance in carrying out the experiments. He also wishes to express his thanks to Mr. I. Errera who instrumented the heat-transfer model, to Mr. R. Zimmerman who performed the programming and supporting calculations on the IBM 7090, and to Drs. E. L. Harris, W. R. Thickstun, and W. E. Parr for many helpful discussions during this work.

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Captain, USN
Commander

K. R. ENKENHUS
By direction

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SYMBOLS

c_m	specific heat of nozzle material
c_p	specific heat of gas at constant pressure
h	local convective heat-transfer coefficient
k_c	reference thermal conductivity
k_t	temperature dependent thermal conductivity
Pr	Prandtl number
q	time rate of heat transfer per unit area
R	dimensionless radial coordinate = r/r^*
R_g	gas constant
r	coordinate in radial direction
r^*	nozzle throat radius
S	transformed radial coordinate
S_1	identification of curve corresponding to higher T_o data
S_2	identification of curve corresponding to lower T_o data
T	temperature
U	transformed temperature
u	velocity
α_t	temperature dependent thermal diffusivity of nozzle material
γ	ratio of specific heats
ρ	density of gas
ρ_m	density of nozzle material
ξ	fraction of space increment ΔS
τ	time
μ	absolute viscosity of gas

Subscripts

a	average fluid conditions
j	space reference in the radial direction
s	nozzle surface conditions
o	supply or stagnation conditions
1, 2, etc.	point identification for numerical solution

Superscripts

n	time reference

INTRODUCTION

The development of higher energy solid propellant rocket motors has produced a corresponding increase in the heat-transfer rate from the exhaust gas to the motor components, and in turn has raised the temperature level of these components. Near the melting temperature of the components, heat must be removed in order to prevent structural failure. More accurate predictions of the quantity of heat transferred from the hot exhaust jet to the internal structure will permit a more efficient design of the motor and cooling system and will increase the probability of a successful mission.

There are many simplified methods for computing the heat-transfer rate from hot gases (refs. (1) through (6)), but little experimental data were available at the start of this work to support the accuracy of these methods when applied to combustion products, such as in the case of solid propellant motors. An important factor in experimental research is the difficulty of accurately measuring temperatures which exceed the melting temperature of thermocouple materials. The present report describes a technique, employing the implicit finite difference approximation of the transient heat conduction equation, to calculate the heat flow in the high-temperature region from the known temperature history in the low-temperature region where instrumentation is practical. Only one-dimensional heat flow is considered in the present analysis, and the method is applied to the flow in the nozzle throat region where maximum heating occurs. The computed heat-transfer rate in terms of Stanton number is compared with several convective heat-transfer methods based on both laminar and turbulent boundary-layer flow.

FORMULATION

Assuming axial symmetry, the general one-dimensional transient heat conduction equation in cylindrical coordinates with variable thermal conductivity and specific heat is:

$$\frac{\partial}{\partial r}\left(k_t \frac{\partial T}{\partial r}\right) + \frac{1}{r}\left(k_t \frac{\partial T}{\partial r}\right) = \rho_m c_m \frac{\partial T}{\partial \tau} \tag{1}$$

Equation (1) can be simplified by applying the following two transformations to adjust for variable thermal conductivity and geometry, respectively:

$$U = \int_{T_{k_c}}^{T} \frac{k_t}{k_c}\, dT \tag{2}$$

and

$$S = \ln R \tag{3}$$

where $R = \frac{r}{r^*}$

Then equation (1) assumes the form:

$$\frac{\partial^2 U}{\partial S^2} = \frac{(Rr^*)^2}{\alpha_t} \frac{\partial U}{\partial \tau} \tag{4}$$

where $\alpha_t = \frac{k_t}{\rho_m c_m}$

Equation (4) can be solved numerically using the implicit finite difference approximation presented in reference (7) which states that for any internal point, j, in the body at a particular instant, n:

$$\frac{(\Delta^2 U)_j^{n+1}}{(\Delta S)^2} = \left[\frac{(U_{j+1} - U_j)^{n+1}}{\Delta S} - \frac{(U_j - U_{j-1})^{n+1}}{\Delta S}\right]\frac{1}{\Delta S} = \frac{(Rr^*)^2}{\alpha_t} \frac{(U_j^{\,n+1} - U_j^{\,n})}{\Delta \tau} \tag{5}$$

where there are three unknown temperatures; namely, U_{j+1}^{n+1}, U_j^{n+1}, and U_{j-1}^{n+1}, and one known temperature, U_j^{n}.

If the body is divided into m segments, then there will be m equation (5)'s with m+2 unknowns. If two of these unknowns, namely, the temperature history at the two boundaries, can be determined experimentally, then the temperature field between the two points can be computed by solving the m algebraic equations with m unknowns. Likewise, the same procedure can be used to extrapolate beyond the measured points toward the surface. At the surface, the convection film coefficient, h, can be computed from Newton's law of cooling and the Fourier heat-conduction equation:

$$q = h(T_a - T_s) = \frac{k_c}{Rr^*}\left(\frac{dU}{dS}\right)_S \tag{6}$$

Reference (7) indicated the truncation error of the implicit numerical solution between the two given boundaries to be of the order of $\Delta\tau + (\Delta S)^2$. The stability of the solution is independent of the values chosen for $\Delta\tau$ and ΔS in contrast with the explicit solution. A general discussion of experimental and numerical accuracy will be presented in the results section.

The numerical solution of equation (5) for six internal points was coded for computation on the IBM 7090 using time increments of 0.1 second. An iterative procedure to compensate for temperature variation of the thermal diffusivity was incorporated in the mechanized solution. A tabulation of the thermodynamic properties of the nozzle material, molybdenum, is shown in Table 1. The temperature transformation of equation (2) to compensate for temperature variation in thermal conductivity is graphed in figure 1. The variation of the thermal diffusivity with transformed temperature of molybdenum is shown in figure 2.

EXPERIMENTAL PROCEDURE AND INSTRUMENTATION

Tests were conducted at the rocket tunnel facility of the Applied Physics Laboratory, Johns Hopkins University, Maryland. The nominal operating supply conditions were 1150 psia and 2500°K which were produced by a standard double base, end burning ARP ten-second propellant prepared by the Allegany Ballistics Laboratory. A detailed discussion of the facility and the flow produced by this propellant is given in references (8) and (9).

The heat-transfer model, one of the standard nozzle configurations used at APL, consisted of a solid molybdenum throat insert that was pressed into a conical steel nozzle. The dimensions of the insert are 2.85 inches long and 0.68 inch-thick at the throat with a convergence of 45 degrees and an expansion angle of 12.5 degrees and throat radius of curvature of 0.77 inch. The nozzle throat and exit diameters were 0.63 inch and 1.99 inches, respectively (see fig. 3).

The throat insert was instrumented with a total of 28 thermocouples in five axial locations and at the interface between the molybdenum and steel as shown by the solid dots in figure 3. All the thermocouples were mounted in one plane passing through the nozzle axis. The thermocouples made from 30-gage platinum and platinum-rhodium wires were mounted in the radial planes in one-degree tapered molybdenum plugs as shown in figure 4. The plugs were inserted into mating holes which bottomed at depths of .010 inch from the gas flow surface (see figure 4).

The thermocouples were anchored in one axial plane of the plug and protruded approximately .001 inch above the surface of the plug. The wires were threaded through a .062 inch diameter hole drilled across the diameter of the plug and were led out of the plug through a channel cut on the opposite face. Having the wire along the diameter is a precaution to minimize any heat losses by conduction through the wire by extending a short segment of the wire along the assumed isotherms. The smallest diameter of the plug was .25 inch and the potting agent was Saureisen No. 76. These plugs were then lap-fitted to the throat insert to insure a good surface contact between each plug and the insert. Thermocouples on the interface between the molybdenum insert and steel shell were mounted into threaded plugs which were screwed into place. The reference thermocouples were formed with copper extension wires kept at room temperature. The emf's of the thermocouples were recorded on a 50-Channel Midwestern Direct Recording Oscillograph, Model 602. The nozzle inlet pressure was recorded with a Statham pressure transducer, and the inlet temperature was measured with two unshielded .020 inch diameter tungsten-iridium thermocouples. One of the two thermocouples was connected to the Midwestern oscillograph, while the other thermocouple and the pressure transducer were connected to Sanborn recorders.

The effect of electrical resistance of the thermocouple lead-wires at elevated temperatures was considered and measured in an electric furnace under simulated test conditions. Wires from the same lot and of the same lengths as those used for the model instrumentation were calibrated against a standard thermocouple calibrated by the National Bureau of Standards. In general, the change in resistance due to uncertainties in the leads at these temperatures as shown in figure 5 is very small compared to the nominal circuit resistance of 230 ohms. The maximum error introduced in the final results is less than one percent.

RESULTS

The characteristic pressure and temperature rise in the combustion chamber is shown in figures 6 and 7, respectively. The two independently measured chamber temperatures represented by curves T_{o1} and T_{o2} in figure 7 showed good initial temperature agreement but departed by approximately ten percent toward the end of the run. The higher temperature curve, T_{o1}, measured on the Midwestern oscillograph had a few saw-tooth type bursts after five seconds duration as shown. The lowest peak of these bursts was approximately four percent below the average. The second temperature data, T_{o2}, were lower and more irregular than the T_{o1} data, and the lower and irregular T_{o2} data appeared to have been caused by the temporary insulation effect of non-gaseous products deposited on the bare thermocouples. The effect of this temperature difference in the heat-transfer results will be shown later.

The maps of isotherms, determined from the data of the thermocouples embedded in the throat insert at time intervals of one, two, three, five, seven and ten seconds, are shown in figures 8 through 10, respectively. The slope of the isotherms in the throat region indicates small axial heat flow. Graphical evaluation of the data shows the second derivative of the temperature in the axial direction to be less than two percent of the second derivative of the temperature in the radial direction. Consequently, it is expected that the temperature and heat-transfer rate at the throat surface can be computed with good accuracy by the one-dimensional heat flow analysis previously described. The computation was performed on the IBM 7090.

Figure 11 is a graph of the radial temperature distribution in the nozzle throat plane. The symbols represent measured temperatures at the location shown. The two temperature boundaries used for the numerical solution were located at the circular and triangular points. The lines plotted represent the computed solution of the transient heat conduction equation at the selected time intervals as described in Appendix A. The heat-transfer rate at the surface was determined from the temperature gradient at the surface. The computed surface temperature and heat flux during the run are shown in figures 12 and 13, respectively. The bands shown represent variations resulting from different interpretation of the data and expected experimental error. They will be discussed more fully later.

The heat transfer to the nozzle throat using the present extrapolation method was compared with several convective heat-transfer theories based on both laminar and turbulent flow (see figure 14). The pertinent equations used for computation are summarized in Appendix A. The upper group of solid curves shows the turbulent heat-transfer rate predicted by the theories of references (1) through (4), using fluid properties corresponding to the average of the surface temperature and the free-stream static temperature. The free-stream static temperature was computed from the known supply temperature and the assumed perfect-gas flow at the nozzle inlet. The equation of Sibulkin (ref. (1)) considers the heat transfer only at the nozzle throat while that of Bartz (ref. (4)) can be applied to other regions of the nozzle. The equations of Dittus and Boelter and of Eckert and Drake (ref. (2)) are relations based on turbulent pipe flow. The curve of Persh and Lee (ref. (3)) is from a step-wise integration of the boundary-layer momentum equation which includes the effect of pressure gradient. Colucci's results of heat-transfer measurements in a rocket nozzle (ref. (10)) were fitted with an equation which coincides with the result of Dittus and Boelter.

The lower group of curves are approximate solutions based on laminar flow over a flat plate (ref. (6)) with selected representative wetted lengths. A wetted length equal to 2.5 times the throat diameter corresponds to the distance from the nozzle throat to the beginning of the conical nozzle inlet; six diameters correspond to the initial burning face of the propellant, and 11.5 diameters corresponds to the burning face of the propellant at 50 percent burn-out. A more exact method for the computation of the laminar heat-transfer rate was presented by reference (5) which included the effect of pressure gradient. One point was computed by this method assuming a surface temperature equal to 90 percent of free-stream stagnation temperature. This is represented by the diamond symbol on the graph.

The computed Reynolds number based on laminar boundary-layer momentum thickness is approximately 650 at the nozzle throat, i.e., it is of a magnitude usually associated with the transition region from laminar to turbulent flow. The present data support the existence of turbulent flow in the throat region and appear to be predicted best by the numerical integration of the boundary-layer momentum equation.

The experimental heat-transfer data in the form of Stanton number are shown by the two curves T_{o1} and T_{o2} of figure 14. The difference between these two curves is due to the difference in supply temperature as was shown in figure 5. The divergence of the experimental data at the higher surface temperature is due to the small temperature differences between the wall and free-stream temperatures and the resulting large relative error in $(T_a - T_s)$ used in equation (6) to compute the heat flux.

Accuracy

The accuracy of the present method for extrapolating the surface temperatures is dependent upon the experimental errors and the truncation error of the numerical solution which was discussed earlier. For the numerical solution on the IBM 7090, the distance between the two boundaries was divided into five segments of equal length on a logarithmic scale and time increments of 0.1 second. Based on past experience of related problems under similar conditions, it was judged that the present selection of increments would give sufficiently accurate results.

Experimental errors may be divided into two classes: those which are systematic or determinate and those which are accidental or indeterminate. The former, which included resistance change of thermocouple wires due to temperature, were minimized by calibration at simulated temperatures as discussed earlier. Indeterminate errors are due to unpredictable effects which are usually lumped together as deviation from some assumed average.

In the present investigation three significant effects classified as indeterminate errors affected the accuracy of the data. One of these was in the insulation of the chamber thermocouple by the unburned propellant. The result of this effect is indicated by the divergence of the two temperature curves as shown in figure 7. The error introduced in the Stanton number correlation is shown by the difference of the T_{O1} and T_{O2} in figure 14. The second effect is the change in contact resistance between the embedded thermocouples and the molybdenum throat insert. Any shifting of the thermocouple during the run usually results in a sudden decrease in emf output when compared to the relatively slow rise of the temperature being measured. Consequently this effect could be easily detected by discontinuities in the temperature-versus-time data recorded. The probability of thermocouple shifting was decreased by welding the thermocouples directly to the tapered plug prior to installation. The third effect, which is characteristic of the nozzle material used, is the fracturing of the insert by the starting thermal shock. These fractures appear as longitudinal hairline cracks located in the throat region and were formed in many of the molybdenum throat inserts tested. Although in the instrumented nozzle the fractures did not impinge directly on the thermocouples, local penetration of the hot gas may have resulted in the high temperatures of some of the internal points. It is speculated that the departure of experimental data from computed results (near the end of the run) as shown in figure 11 was due to local gas flow, i.e., the internal thermocouples were heated by hot gas leaking through the fractures.

The human factor in drawing the curve through the points of figure 11 is illustrated in figure 15. The bands shown represent the spread of curves made by ten independent interpretations of the data. The resultant scatter at the end points was incorporated into the numerical solution and the uncertainties at the throat surface are shown by the cross-hatched areas in figures 12, 13, and 14.

CONCLUSIONS

A combined experimental and analytical method is given for obtaining the surface heat-transfer rate at the throat of a rocket nozzle. Experimental data justify the use of the one-dimensional heat flow analysis. The results are compared with theoretical predictions and with other available experimental results. Good agreement is obtained with turbulent heat-transfer rates computed from the point-by-point solution of the boundary-layer momentum equation along the nozzle contour.

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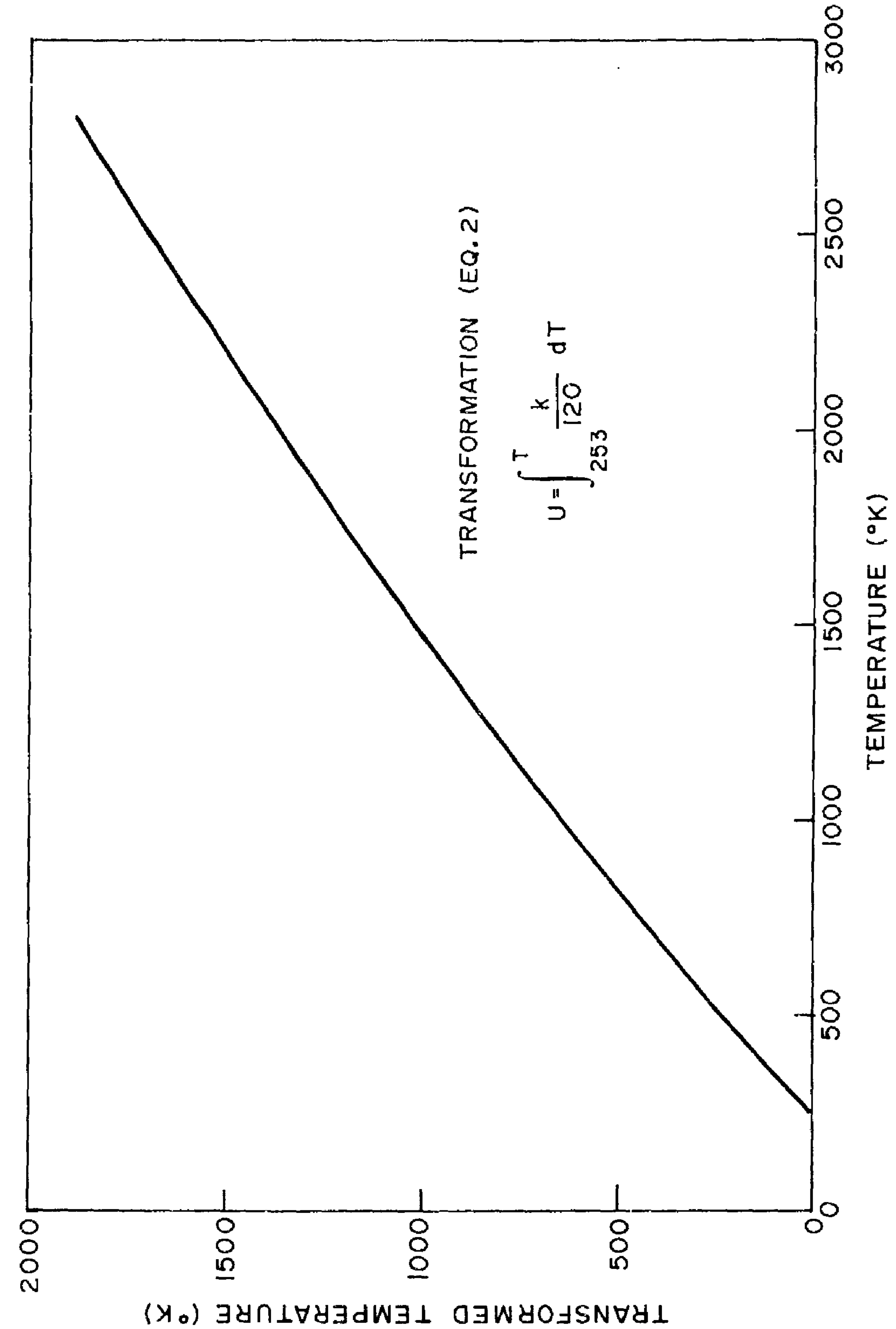

FIG. 1 TEMPERATURE TRANSFORMATION FOR MOLYBDENUM

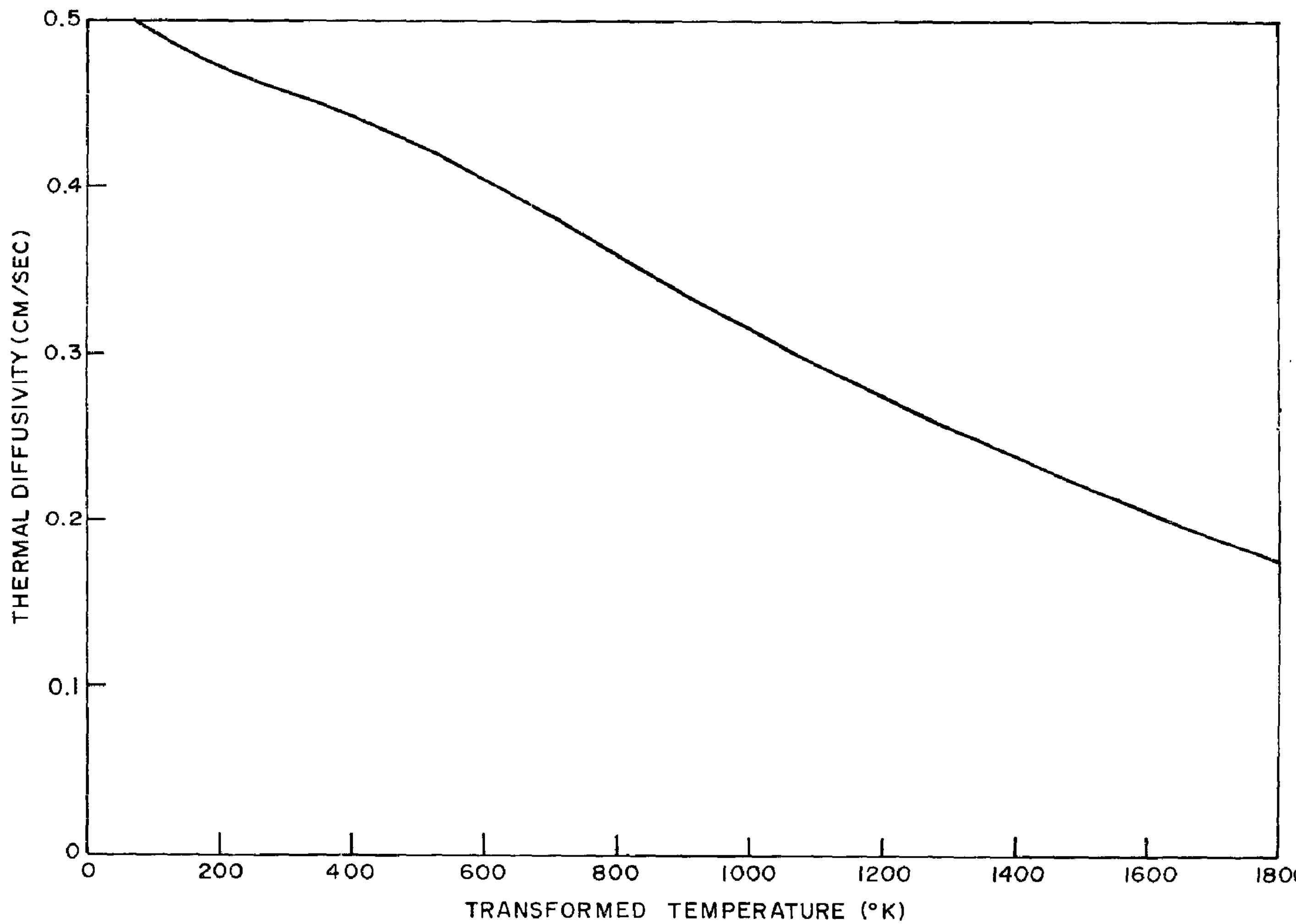

FIG. 2 VARIATION OF THERMAL DIFFUSIVITY WITH TRANSFORMED TEMPERATURE FOR MOLYBDENUM

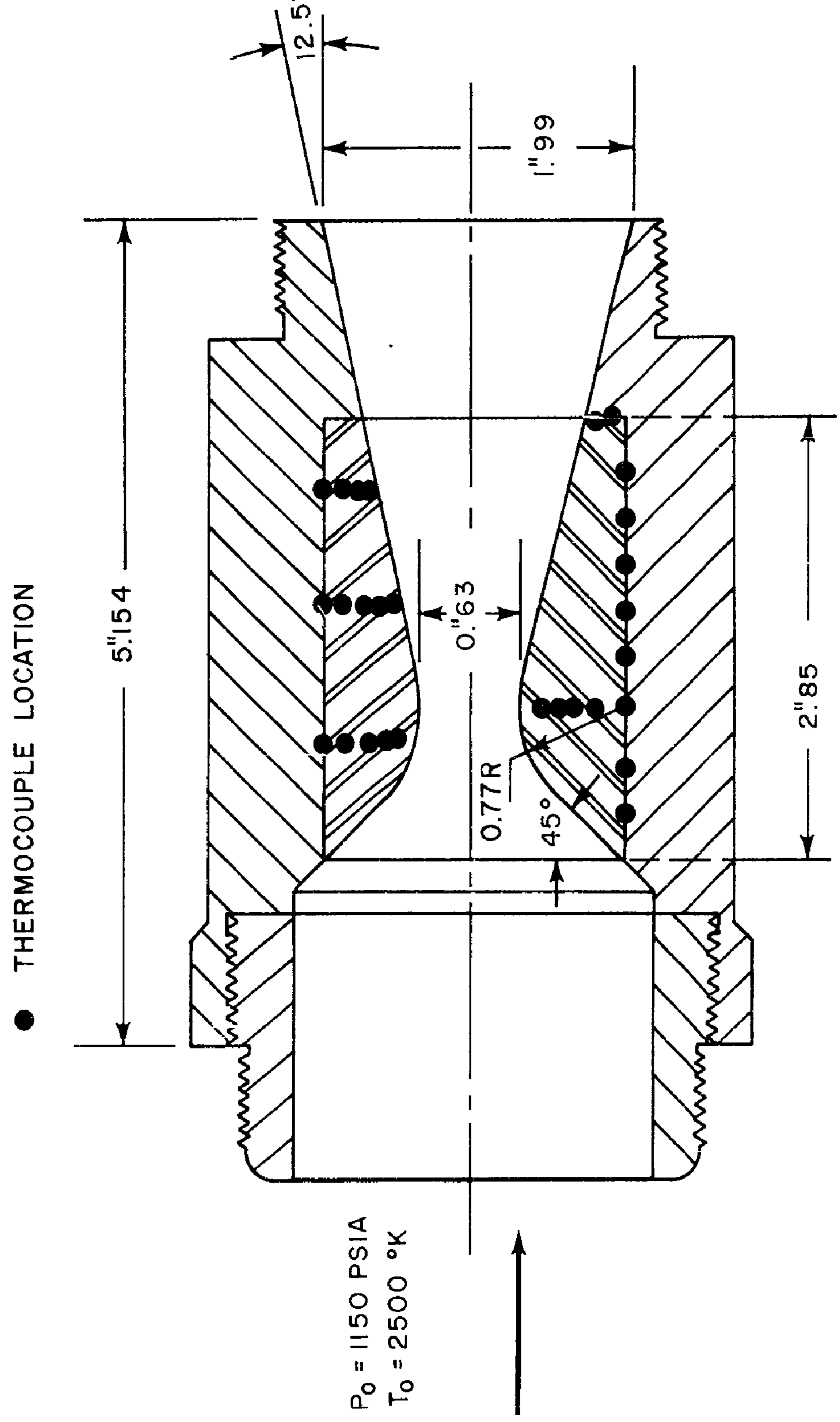

FIG. 3 INSTRUMENTED ROCKET NOZZLE

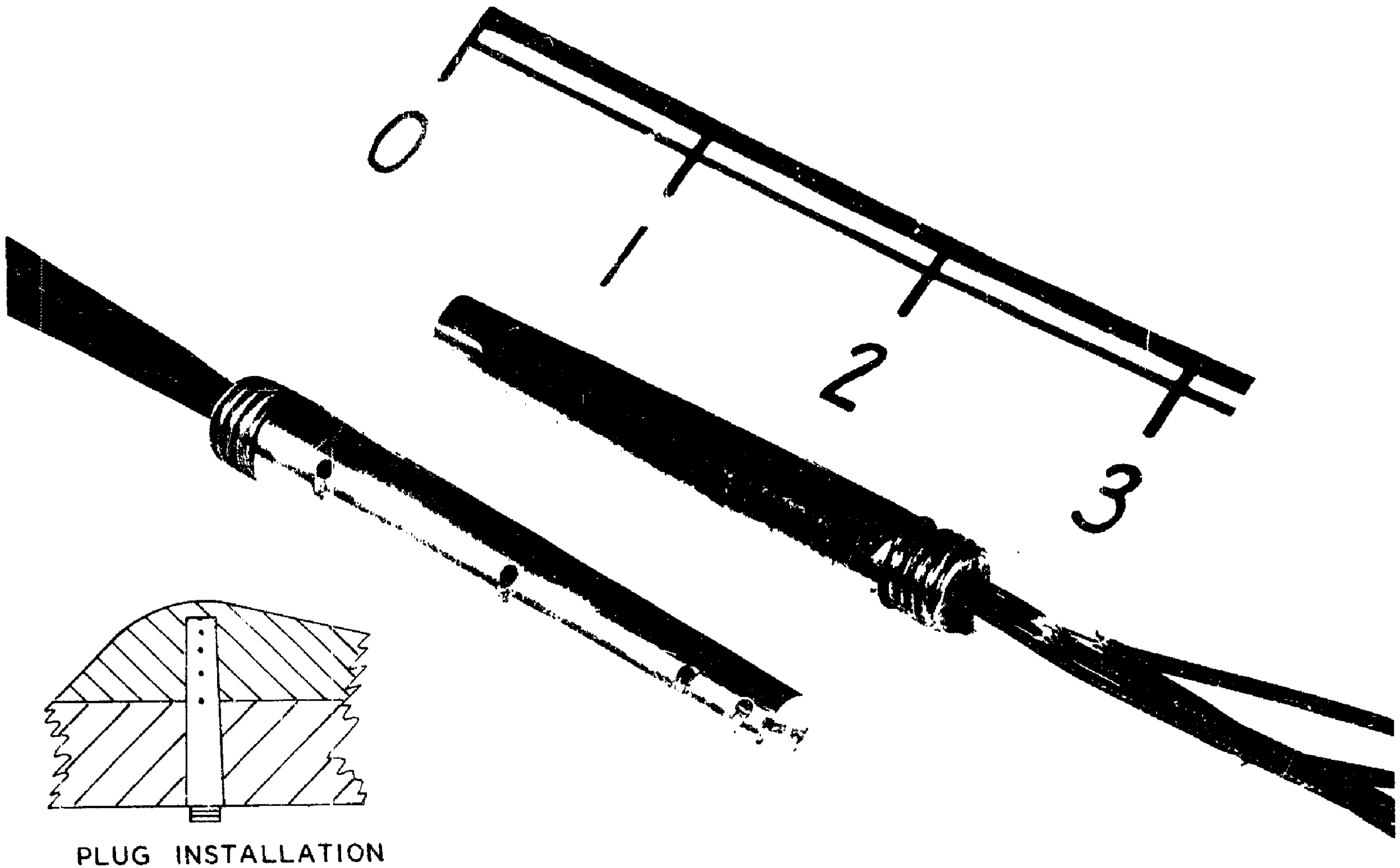

FIG. 4 PHOTOGRAPH OF THERMOCOUPLES MOUNTED INTO TAPERED MOLYBDENUM PLUGS

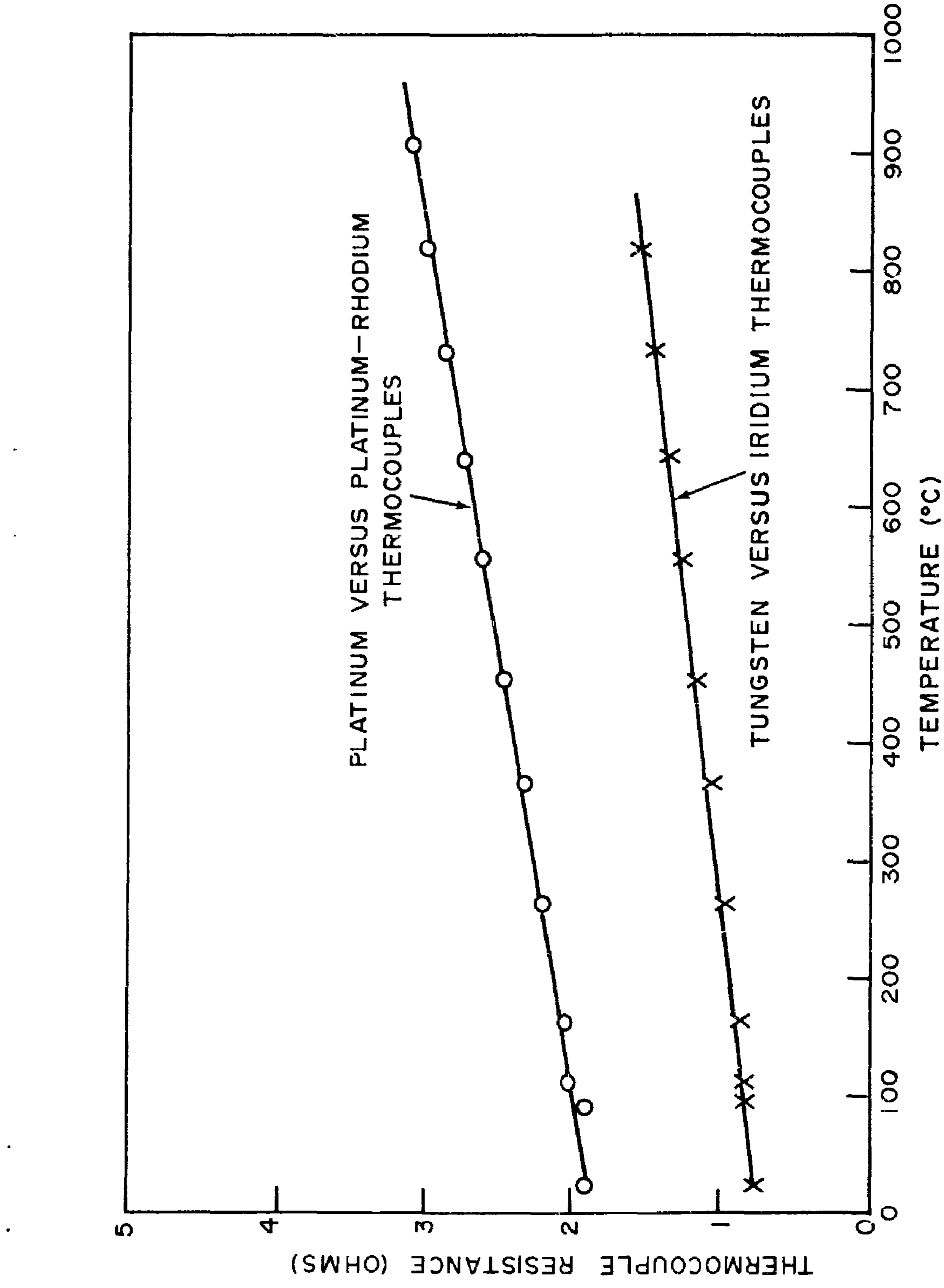

FIG. 5 EFFECT OF TEMPERATURE ON THERMOCOUPLE RESISTANCE FOR 0.010" WIRES

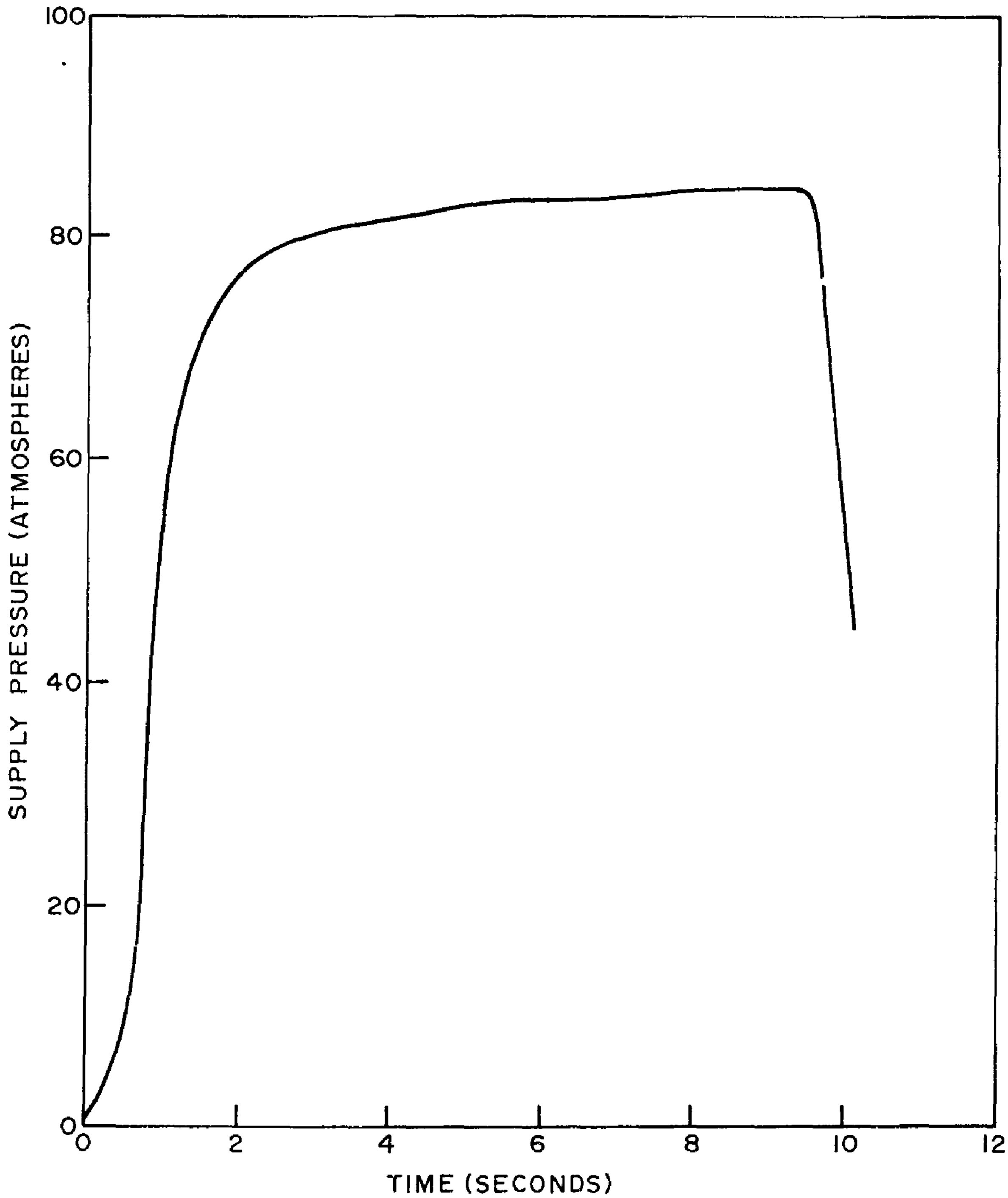

FIG. 6 SUPPLY PRESSURE CHANGE AS FUNCTION OF TIME

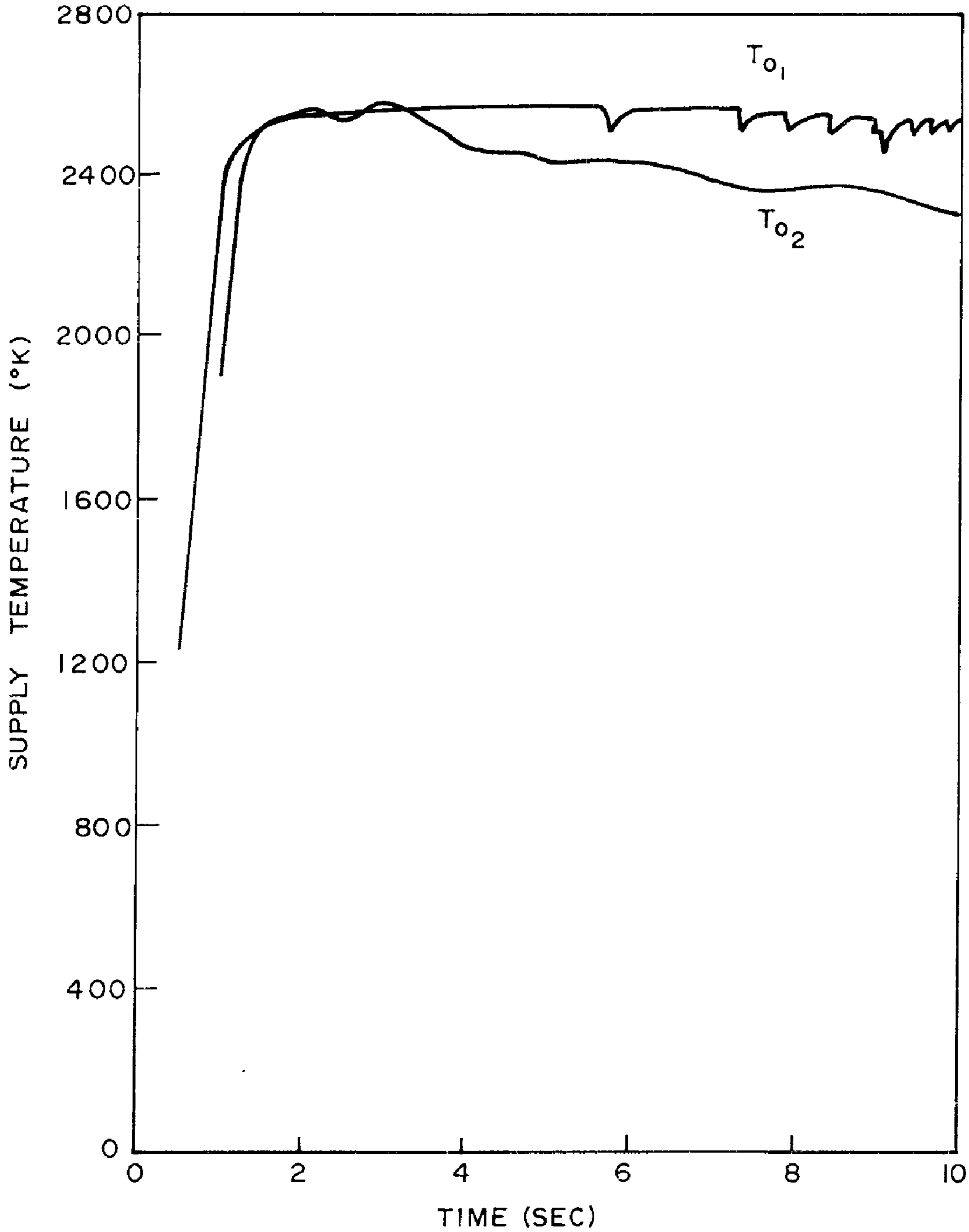

FIG. 7 CHAMBER TEMPERATURE VERSUS TIME

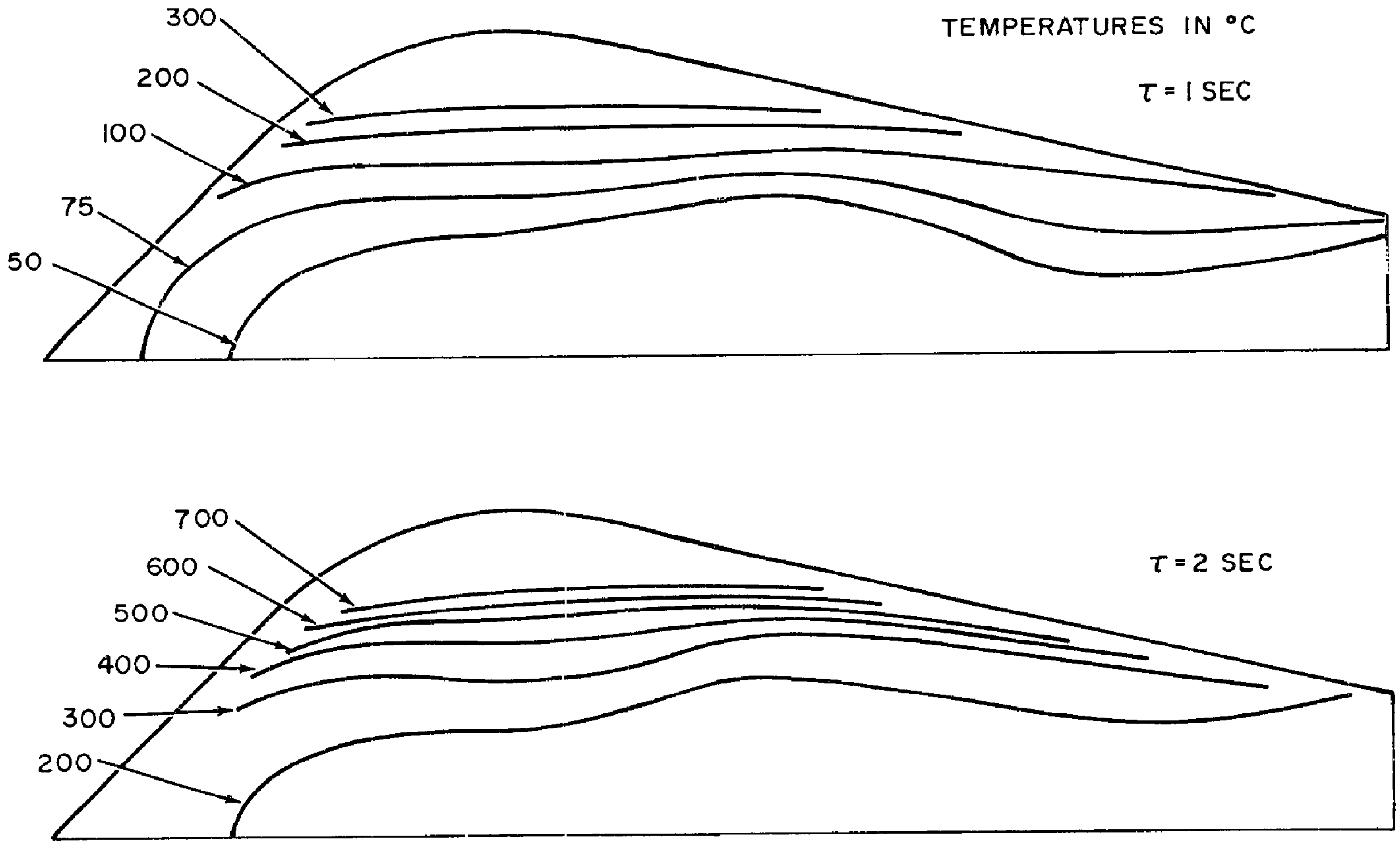

FIG. 8 ISOTHERMS IN THE MOLYBDENUM THROAT INSERT AT 1 AND 2 SECONDS AFTER START OF RUN

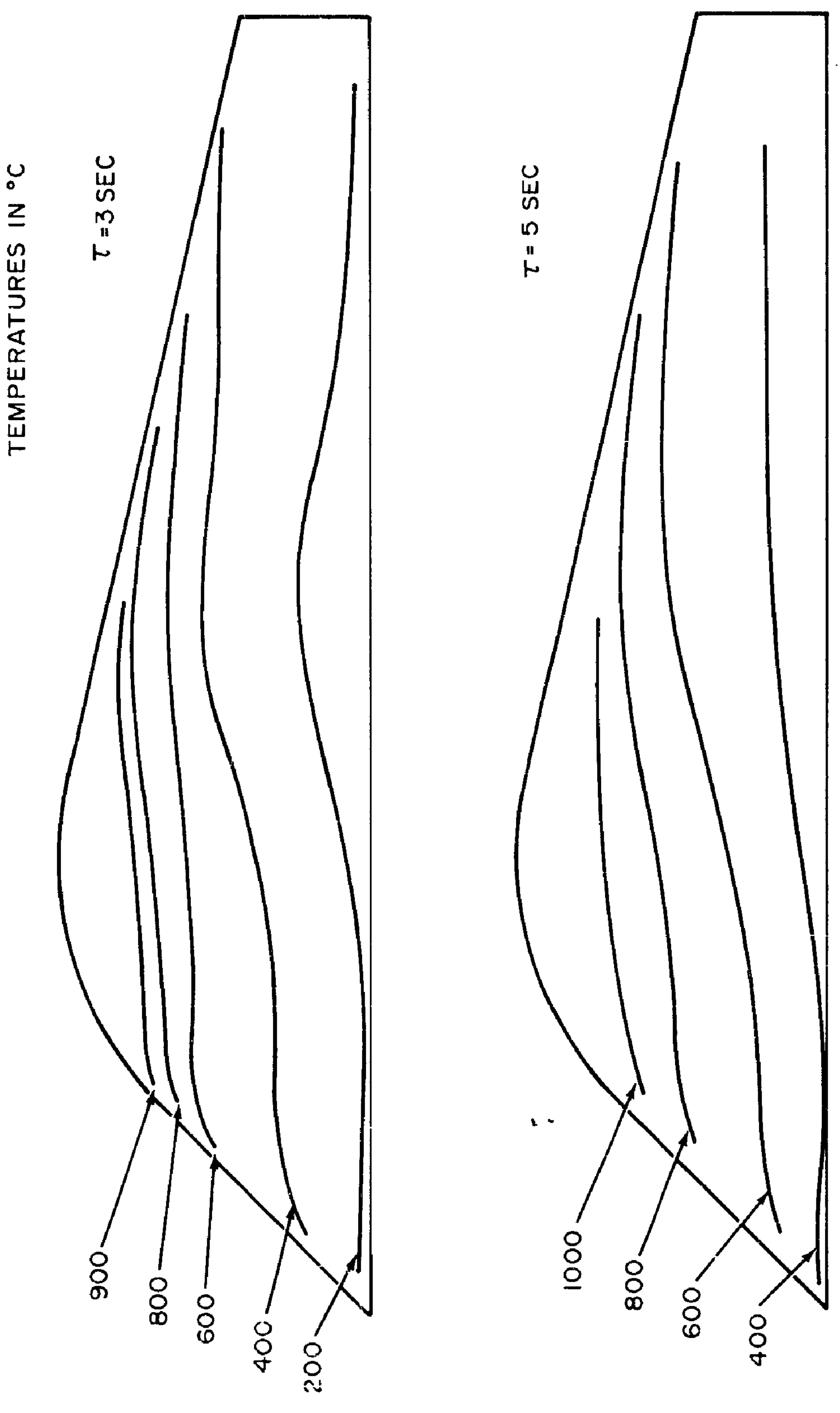

FIG. 9 ISOTHERMS IN THE MOLYBDENUM THROAT INSERT AT 3 AND 5 SECONDS AFTER START OF RUN

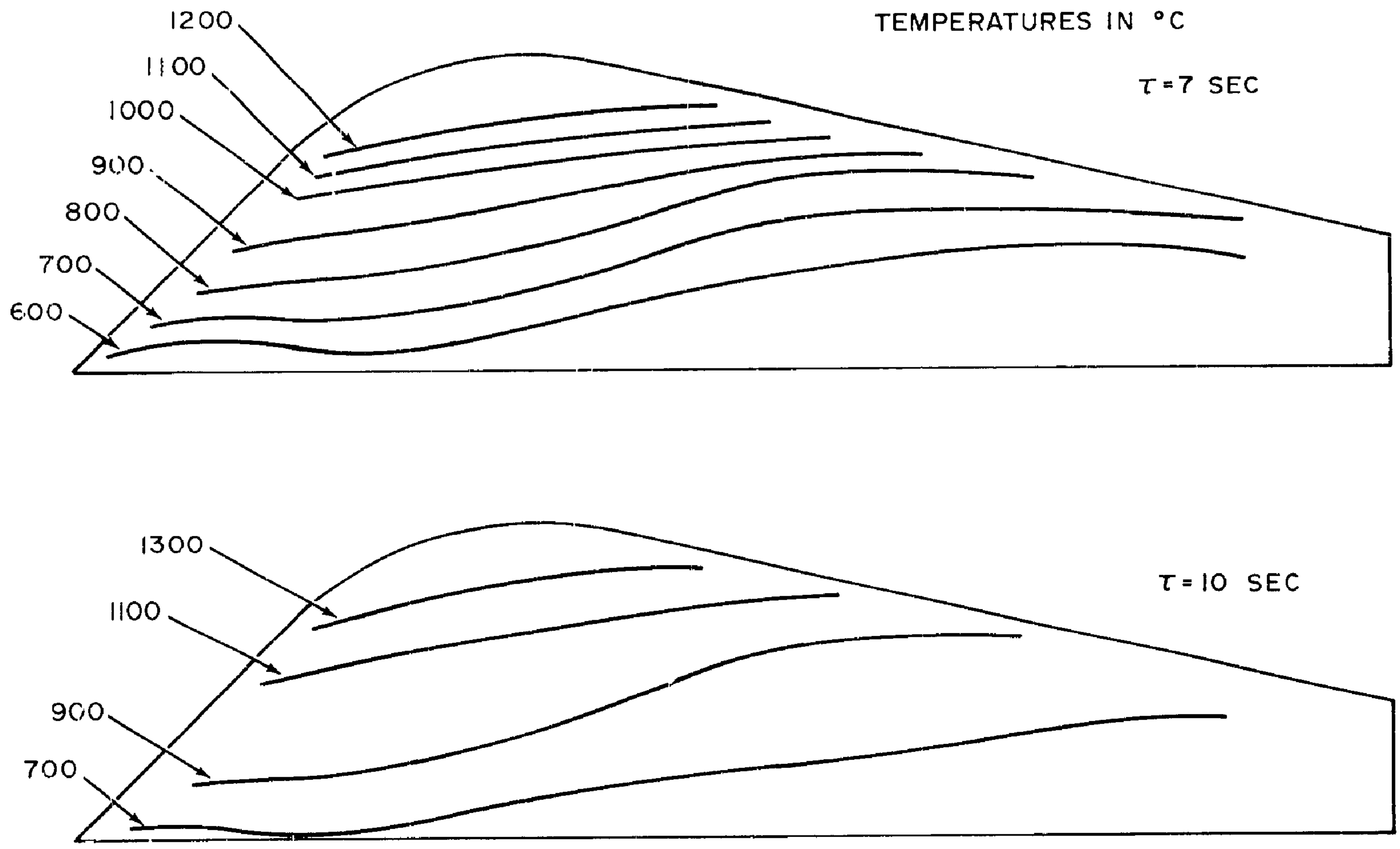

FIG. 10 ISOTHERMS IN THE MOLYBDENUM THROAT INSERT AT 7 AND 10 SECONDS AFTER START OF RUN

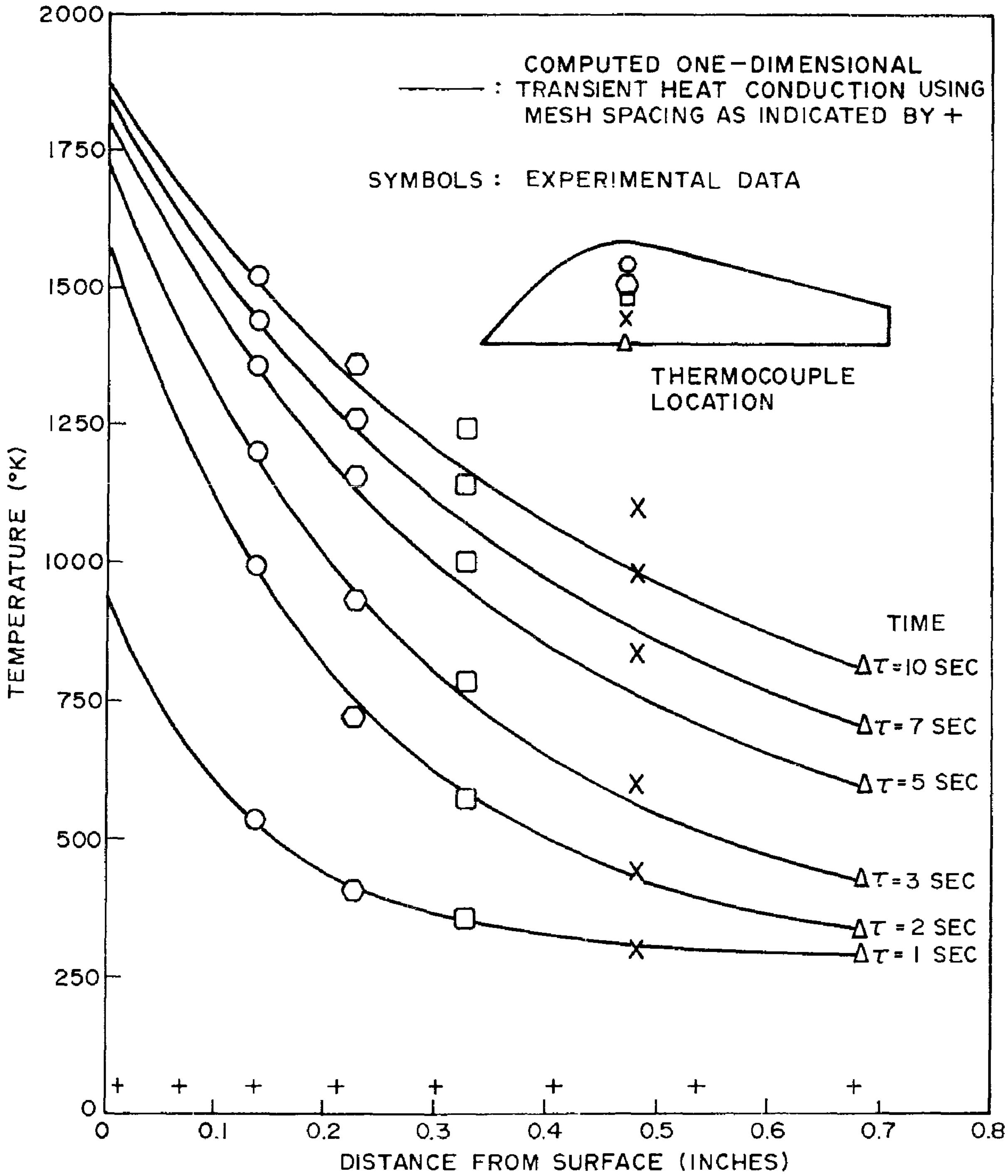

FIG. 11 TEMPERATURE DISTRIBUTION IN THE NOZZLE THROAT PLANE

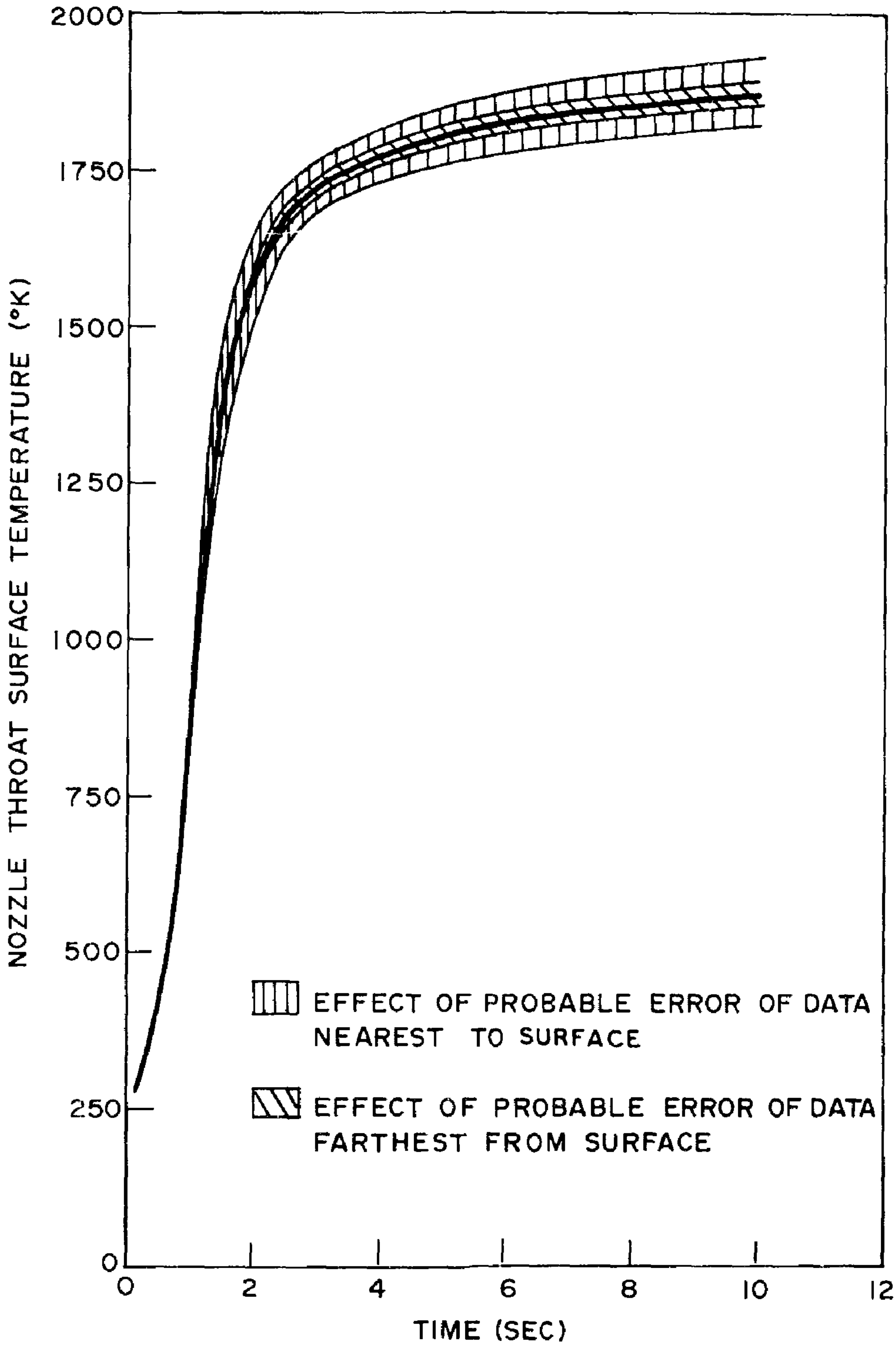

FIG. 12 NOZZLE THROAT SURFACE TEMPERATURE RISE

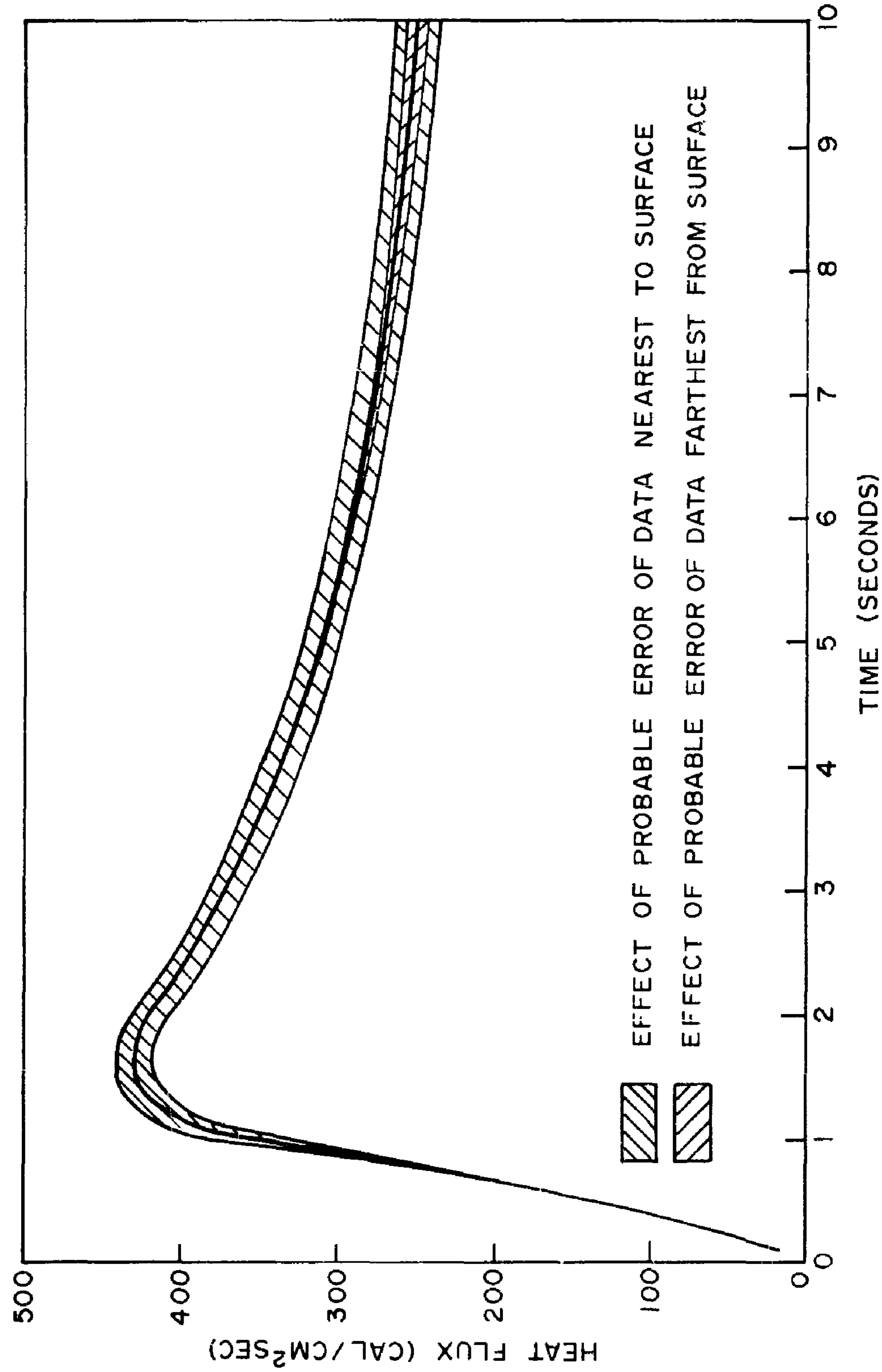

FIG. 13 HEAT FLUX AT THE NOZZLE THROAT SURFACE

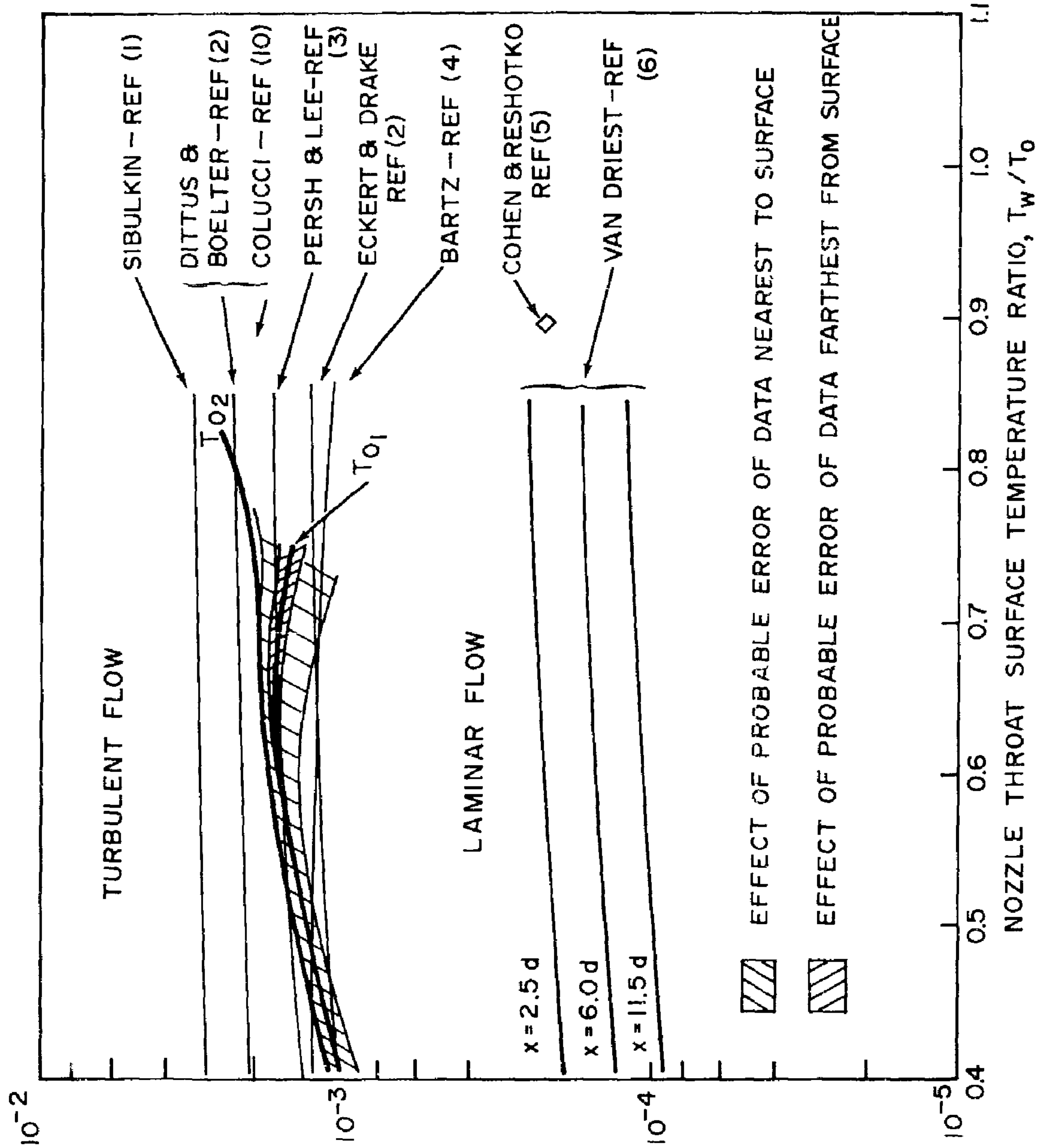

FIG. 14 STANTON NUMBER CORRELATION AT THE NOZZLE THROAT SURFACE

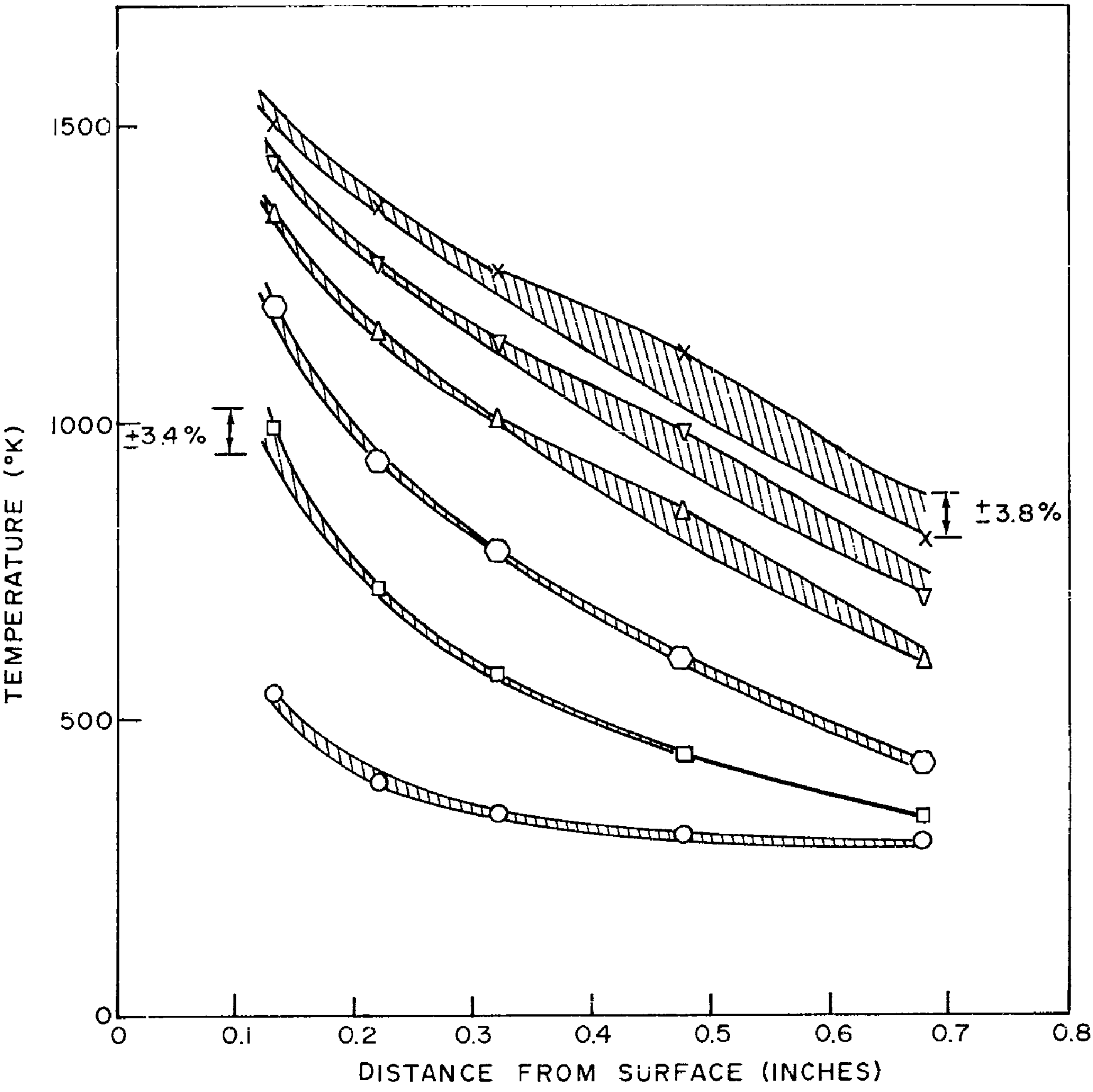

FIG. 15 RANGE OF INTERPRETATION OF TEMPERATURE DATA IN THE NOZZLE THROAT PLANE

Table 1

MATERIAL PROPERTY DATA - MOLYBDENUM

Melting Point: 5200°Rankine = 2888° Kelvin
Emissivity : 0.10
Density : 9.9371 gm/cm^3

Temperature (deg Kelvin)	Conductivity (cal/cm sec °C)	Specific Heat (cal/gm°C)
253	.333	0.065
273	.328	0.065
477	.302	0.065
588	.289	0.064
699	.281	0.064
810	.273	0.064
921	.264	0.065
1033	.256	0.067
1144	.252	0.069
1255	.247	0.071
1366	.243	0.074
1477	.239	0.077
1588	.235	0.080
1699	.231	0.083
1810	.231	0.086
1921	.227	0.090
2032	.227	0.094
2144	.222	0.098
2255	.222	0.103
2366	.218	0.108
2477	.218	0.113
2588	.218	0.119
2699	.214	0.124
2810	.214	0.130

APPENDIX A

Summary of Theories Used for Heat-Transfer Correlation

Heat transfer was correlated in terms of Stanton number which is defined as:

$$St = \frac{h}{\rho u c_p} = \frac{Nu}{Re\ Pr} \qquad \text{(A-1)}$$

Computations were based on isentropic flow in the region between the combustion chamber and the nozzle throat and the following gas properties obtained from references (8) and (9):

$\gamma = 1.25$

$C_p = .424$ Btu/lb°R

$R_g = 63.173$ ft-lb/lb°R

$\mu_o = 4.81 \times 10^{-5}$ lb/ft-sec

$Pr = .607$

The values of the convective heat-transfer coefficient, h, were computed from equation (6). The method for computing the theoretical values of h are listed as follows:

a. Sibulkin, reference (1) - (applies to nozzle throat only)

$$St = \frac{1}{\bar{\rho} u_1 c_p}\left[\frac{C_2 p_o (\nu^*)^{1/5}}{T_o^{3/5}(r^* L^*)^{1/10}}\right]\left(\frac{T_1}{\bar{T}}\right)^* \qquad \text{(A-2)}$$

where:

$$C_2 = .0226\left(\frac{2}{\gamma+1}\right)^{1/2} \frac{\gamma^{2/5}}{R_g^{3/5}} \frac{C_p}{Pr^{2/3}} \qquad \text{(A-3)}$$

γ - ratio of specific heaters

R_g - gas constant, ft^2/sec^2°R

C_p - specific heat at constant pressure, Btu/slug°R

Pr - Prandtl number

p_o = supply pressure, lb/ft^2

ν^* = kinematic viscosity, ft^2/sec

T_o = supply temperature, ^oR

T_1 = stream temperature, ^oR

$\overline{T} = \frac{T_o + T_w}{2}$, ^oR

T_w = wall temperature, ^oR

r^* = radius of nozzle throat opening, ft

L^* = radius of nozzle throat curvature, ft

$\overline{\rho}$ = density at $\overline{T}$, $lb\text{-}sec^2/ft^4$

u_1 = local stream velocity

b. Dittus and Boelter, reference (2)

$$St = .0265 \left(\overline{Re}_d\right)^{-.2} \left(Pr\right)^{-.7} \tag{A-4}$$

Pr = Prandtl number

$\overline{Re}_d$ = Reynolds number based on local diameter, local stream velocity, density and viscosity at $\overline{T}$.

c. Colucci, reference (10) - curve drawn through experimental data

$$St = .023 \left(\overline{Re}_d\right)^{-.2} Pr^{-1} \tag{A-5}$$

d. Persh and Lee, reference (3) - The Colburn form of Reynolds analogy

$$St = \frac{c_f}{2} Pr^{-2/3} \tag{A-6}$$

is used to obtain the heat-transfer coefficient. The local skin friction coefficient is obtained from its assumed dependency on the boundary-layer momentum thickness given in reference (3), and the numerical integration of the boundary-layer momentum equation.

e. Eckert and Drake, reference (2)

$$St = \frac{.0384 \left(Re_d\right)^{-\frac{1}{4}}}{1 + \left(1.5\, Pr^{-1/6}\right)\left(\overline{Re}_d^{\,-1/8}\right)\left(Pr - 1\right)} \tag{A-7}$$

f. Bartz, reference (4)

$$St = \frac{1}{\bar{\rho} u_1 c_p} \left[\frac{0.026}{D_*^{0.2}} \left(\frac{\mu^{0.2} c_p}{Pr^{0.6}} \right)_0 \left(\frac{p_c g}{C^*} \right)^{0.8} \left(\frac{D_*}{r_c} \right)^{0.1} \right] \left(\frac{A_*}{A} \right)^{0.9} \sigma \qquad \text{(A-8)}$$

D_* = throat diameter

μ = viscosity

c_p = specific heat at constant pressure

$(\ )_0$ = stagnation condition

p_c = chamber pressure

g = gravitational acceleration

C^* = characteristic velocity

r_c = throat radius of curvature

σ = dimensionless factor accounting for variation of ρ and μ values across boundary layer

g. Cohen and Reshotko, reference (5)

$$St = \frac{1}{Pr^{1-\alpha} \sqrt{Re_w}} \left[\frac{2\ell \sqrt{\frac{x}{L} \frac{P'(t_0/t_e)}{n}}}{\left(\frac{C_f \, Re_w}{Nu} \right)_{Pr=1}} \right] \qquad \text{(A-9)}$$

Pr = Prandtl number

α = exponent of Prandtl number in Reynolds analogy parameter

Re_w = Reynolds number, $Re_w = \dfrac{\rho_w u_e x}{\mu_w}$

P' = dimensionless pressure gradient

$\dfrac{x}{L}$ = dimensionless surface coordinate

$\dfrac{t_0}{t_e}$ = ratio of stagnation to free-stream temperatures

C_f = local skin-friction coefficient

n = correlation number

Nu = Nusselt number

h. Van Driest, reference (6)

$$St = 0.332\ Re_x^{-\frac{1}{2}}\ Pr^{-2/3} \tag{A-10}$$

Re_x = Reynolds number based on the assumed wetted length x and the density and viscosity computed at the mean temperature between the surface and free-stream.

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